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Design Development Analyses in Support of a Heat Pipe-Brayton Cycle Heat Exchanger

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May 2004

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LIST OF ACRONYMS AND SYMBOLS

Ar	argon
ASME	American Society of Mechanical Engineers
BPVC	boiler and pressure vessel code
CAD	computer-aided design
CFD	computational fluid dynamics
FLUINT	fluid integrator (program)
He	helium
HX	heat exchanger
MCNPX	Monte Carlo n-particle transport code system
PF	power factor
P in	pressure in
SAFE	Safe, Affordable Fission Engine
SINDA	systems improved numerical difference analyzer (program)
T in	temperature in
T out	temperature out
VH	velocity head
Xe	xenon

NOMENCLATURE

D	total allowable damage
h	rib dimension
j	cycle type index
k	time interval index
n	number of applied cycles for cycle type j
N_d	number of allowable cycles for cycle type j based on low cycle fatigue life
p	distance between ribs; total number of cycle types
q	total number of time intervals
T_d	allowable time duration for time interval k based on stress-to-rupture
$\Delta P/P$	pressure drop
Δt	duration of time interval k

TECHNICAL MEMORANDUM

DESIGN DEVELOPMENT ANALYSES IN SUPPORT OF A HEAT PIPE-BRAYTON CYCLE HEAT EXCHANGER

1. INTRODUCTION

Due to the benefits associated with their high-energy density, fission power systems are currently under consideration for in-space applications. One such system is a heat pipe reactor coupled to a Brayton cycle engine. To make the conversion from the reactor heat generation to electric power, the thermal energy must be transferred from the reactor to the working fluid of the Brayton cycle by a heat exchanger (HX). In the heat pipe-Brayton system, the HX connects to reactor heat pipes and heats the gas mixture to the desired temperature for the Brayton cycle. The design of such an HX must consider the requirements associated with an in-space system such as high temperatures, long life, and size and weight restrictions.

An in-space fission power system is developed by ground-testing, evaluating, and improving candidate systems. Because of the high costs and complexities associated with nuclear testing, these tests are proposed to be conducted using nonnuclear heating. One such test uses the Safe, Affordable Fission Engine (SAFE–100), a 19-module, stainless steel core and HX, which is currently under construction and will be tested at NASA’s Advanced Propulsion Thermal-Hydraulic Simulator Facility at Marshall Space Flight Center. Figure 1 illustrates how the core and HX will be configured during testing.

This Technical Memorandum presents the thermal, fluid, and structural analyses performed in support of the HX design, for both the test model and a possible reactor flight design. The capability of this design to be upgraded to higher power operating levels is also discussed.

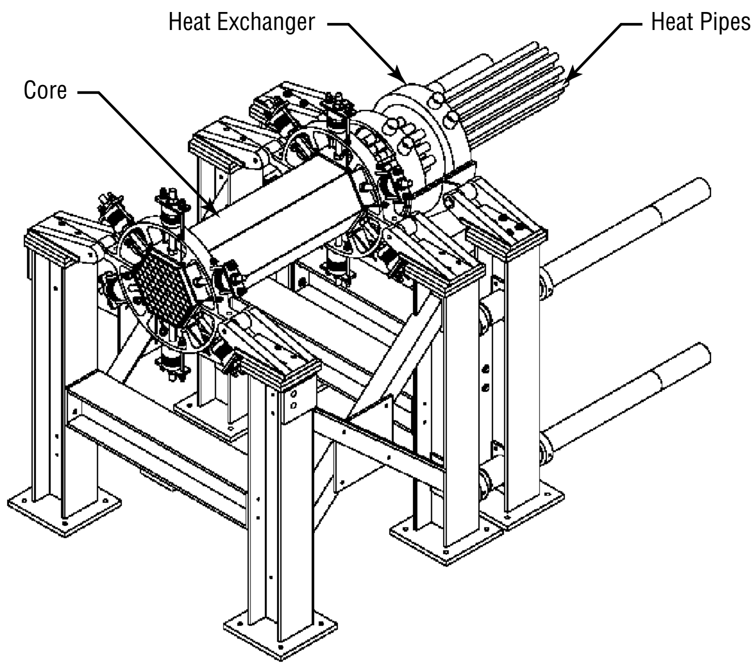

Figure 1. SAFE–100 experimental setup.

2. DESIGN DESCRIPTION

Figure 2 illustrates the HX for the SAFE–100 test model. The annular flow design is fabricated from 316L stainless steel as a separate unit, pressure tested, and slid over the condenser ends of the heat pipes extending from the core. The gaps between the heat pipes and the HX are filled either with helium (He) or by brazing the HX to the heat pipes. The flow enters the HX at the top, passes into a flow distribution ring, and crosses into the upper plenum. From there, it traverses the 19 annular flow passages, each passage surrounding a heat pipe. The flow then exits the annular flow passages, recombines in the lower plenum, crosses back into the lower flow distribution ring, and then flows into the coolant return pipes.

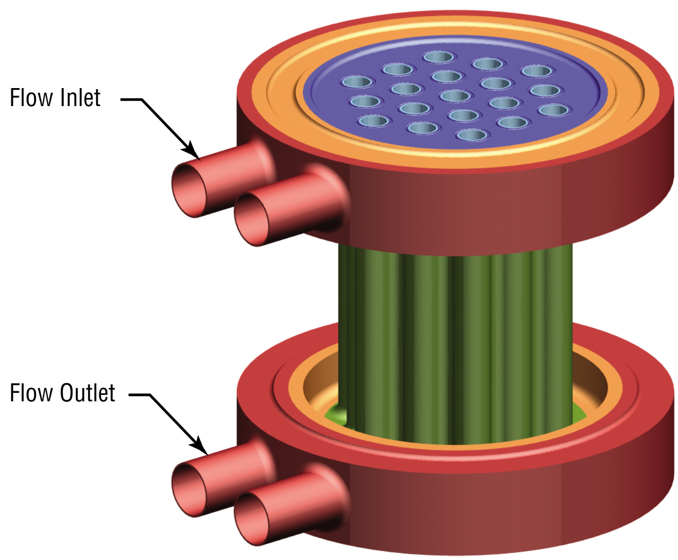

Figure 2. SAFE–100 heat exchanger design.

Figure 3 shows the HX subcomponents, which include heat pipe sleeves, the center section block, the upper and lower flow distribution chambers, and the upper and lower cover plates. The annular flow passages are formed by the sleeves on the inside and by the circular channels in the center section block on the outside. Circular ribs in the flow annuli on the sleeve side disrupt the boundary layer, enhancing the heat transfer coefficient and ensuring turbulent flow down to a Reynolds number of ≈2,500. Gufee[1] describes the HX design and fabrication in detail.

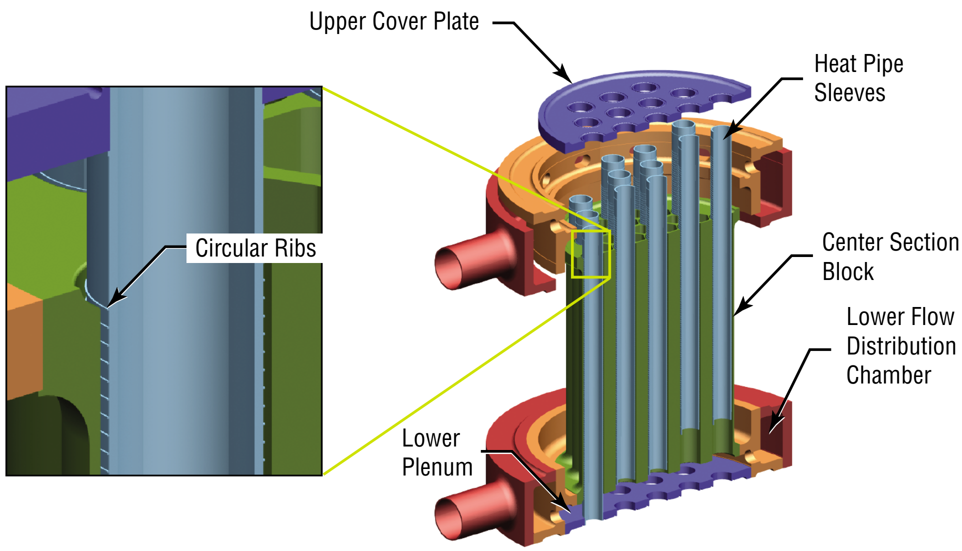

Figure 3. Partially exploded view of heat exchanger.

For cost efficiency, the full-scale HX test model comprises only a partial array of heat pipe modules (19 in the test versus 61 in the reactor). The use of a partial array is justified by the fact that the flow annuli in the test model, where most of the heat transfer takes place, are geometrically identical to those in the reactor HX. The dimensions of the flow distribution chamber and the height of the inlet and exit plenums have been reduced in the test model to provide approximate thermal-hydraulic similarity to the reactor HX. Further, as discussed below, computational fluid dynamic (CFD) analyses are planned to compare flow distributions in the test and reactor models, and stress analyses have been completed for the reactor design as well as the test model. The results from these stress analyses demonstrate that the 19-heat pipe test model produces similar stress values in the critical locations in the HX.

3. FLUID AND THERMAL ANALYSIS

3.1 Test Conditions

The operating conditions for the SAFE–100 reactor and HX are presented in table 1. The core power level is based on mission requirements. The core dimensions are driven by the nuclear design. The coolant composition and conditions (temperature in (T in), temperature out (T out), pressure, pressure drop) are based on recommendations from Glenn Research Center for a Brayton cycle, with the outlet temperature of 850 K estimated as the maximum envisioned for a stainless steel design. The dimensions of the heat pipe modules were selected to produce acceptable core operating temperatures and stresses.

Table 1. SAFE–100 reactor operating conditions.

Condition	Value
Power	100 kW
T in	669 K
T out	850 K
Pressure	1.38 MPa
Gas	72% He/28% Xe
Number of heat pipes	61
Heat pipe diameter	0.625 in
Heat pipe pitch	1.25 in
$\Delta P/P$	<0.025

The reactor arrangement consists of two HXs attached to the heat pipes, each removing 50 percent of the core power. In the test, only one HX will be used, so that when the HX operates at full power, the core will operate at half power. This test configuration saves test costs while producing similar core temperatures and stresses to those in a reactor core at full power.[2]

To further reduce costs, the He/xenon (Xe) mixture in the reactor Brayton system was replaced with an He/argon (Ar) mixture. A mixture of 20 percent He and 80 percent Ar was selected so that the Reynolds numbers in the test are about the same as in the reactor HX, as are dynamic heads, Mach numbers, and pressure drops. However, heat transfer coefficients are ≈40 percent lower. As a result, for the same coolant operating conditions, the heat pipe and core temperatures in the test model are ≈20 K higher than those in a flight reactor. This increase was considered acceptable.

Before brazing the HX to the heat pipes, a series of tests is proposed where the HX is slid onto the heat pipes and the test chamber is filled with He. These tests will serve to assess the benefit of He between the core modules in reducing temperatures in the event of a failed heat pipe. For these tests, the coolant temperatures will be reduced by 70 K to offset the greater temperature across the gap between the heat pipe and the HX sleeve. The coolant temperature rise, a key driver in the HX thermal stresses, will be maintained at 181 K.

3.2 Method

Figure 4 illustrates the method used for the HX analysis. In this method, initial parametric analyses are performed using a simplified model contained in a Microsoft® Excel spreadsheet. A single annular channel is analyzed, and the channel geometry is varied to identify the optimum design per the design requirements. The key geometry variables are coolant channel width and length. Several performance parameters are tracked to ensure acceptable performance: heat pipe temperature, pressure drop, Reynolds number, Mach number, and coolant velocity. The heat pipe powers vary with the core radial power distribution. To produce near-uniform coolant temperature rises in the HX coolant passages, the dimensions of the flow annuli are varied with the core radial power distribution. These calculations are also performed using the Excel spreadsheet. The results from the spreadsheet analysis are used as boundary conditions for the detailed, finite element analysis using ANSYS®.

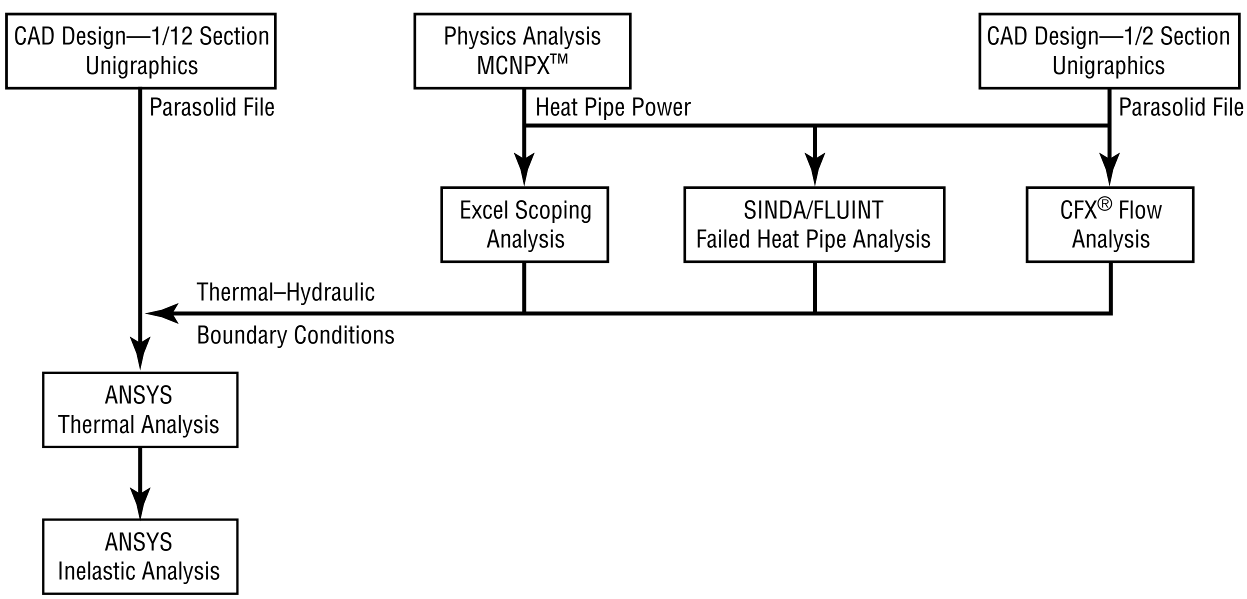

Figure 4. Heat exchanger thermal/stress analysis procedure.

For a failed heat pipe event, the coolant in the annular channel surrounding the failed heat pipe is heated only by heat transferred from surrounding heat pipes, through the connecting webs in the center section block. A SINDA/FLUINT model consisting of the failed heat pipe channel and the six surrounding channels was developed to perform this analysis. As with the Excel spreadsheet analysis, the results from this analysis are used as boundary conditions for follow-on, detailed finite element analysis.

The HX includes flow distribution rings at the entrance and exit, which connect to the inlet and exit plenums. The flow passages in these rings and in the plenums are sized to provide acceptable pressure drop and flow distribution among the annular flow channels. Confirmatory analysis is performed using a CFD flow model of a symmetric half-section of the HX. (This has yet to be completed.)

The flow annulus includes ribs on the sleeve side to enhance heat transfer between the coolant and the heat pipe. The ribs are square in cross section, with a dimension of about one-seventh of the channel width, and a p/h spacing of 10:1, where p is the distance between ribs and h is the rib dimension. The correlations for channel friction factor and heat transfer for this flow channel were taken from Takase.[3]

3.3 Results

Figure 5 illustrates the results from the parametric analysis for the SAFE–100 reactor HX. Heat pipe vapor temperatures are plotted as a function of system pressure for a series of HX annulus lengths. For these calculations, the power and coolant temperatures were held constant, and a gas mixture of 72 percent He/28 percent Xe was used. For each calculation, the channel width was adjusted to produce a fractional pressure drop of $\Delta P/P = 0.015$. The pressure drop limit is $\Delta P/P = 0.025$, but 0.01 of this limit is allocated to pressure losses in the inlet and exit plenums. Heat pipe temperatures are reduced as system pressure and coolant channel length increase. However, as the heat pipe temperature approaches the coolant temperature, the effect of increased pressure and/or increased channel length becomes small. Based on these results, a system pressure of 1.38 MPa (maximum recommended for the Brayton system) and a channel length of 0.2 m were selected for the HX design.

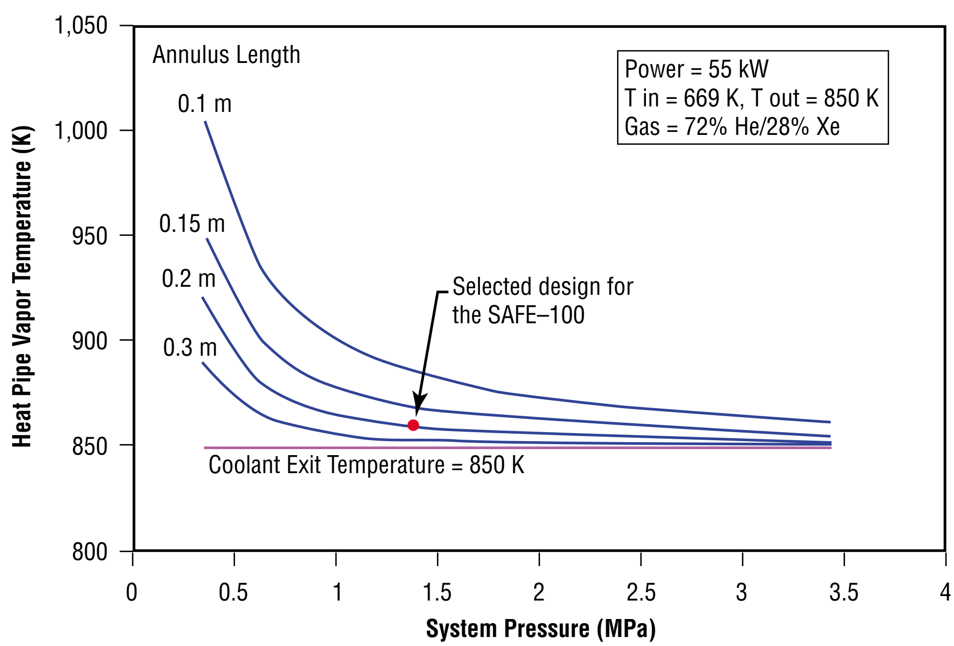

Figure 5. Heat exchanger parametric study.

Temperature profiles through the coolant flow annulus at a peak power location are presented in figure 6. The temperature rise from the coolant into the heat pipe is small at the channel exit, only

10 K. Thus the heat pipe vapor temperature, which is the boundary condition temperature in the core, is close to the coolant exit temperature. The operating conditions and performance parameters for the coolant flow through this channel are also illustrated in figure 6. Reynolds numbers are turbulent, varying from over 7,200 at entrance to slightly less than 6,200 at exit. Mach number is low, and the flow is noncompressed. Coolant velocities are relatively low. These analyses were repeated for each of the proposed test conditions to provide input boundary conditions for the structural analyses.

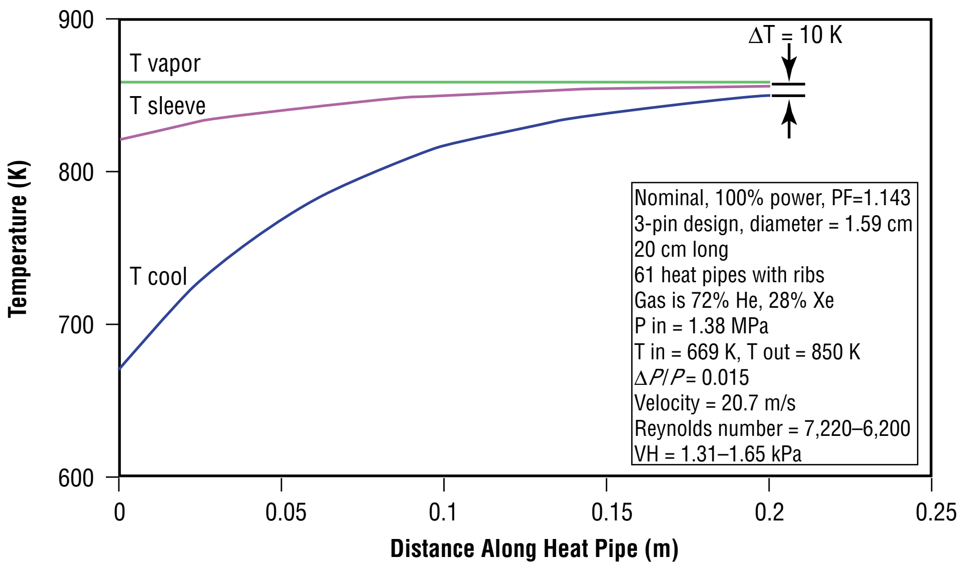

Figure 6. SAFE–100 reactor performance results.

4. STRUCTURAL ANALYSIS

4.1 Method

The purpose of the structural analysis was to evaluate the HX design's ability to withstand both time-independent and time-dependent failure modes under the SAFE–100 experiment pressure and thermal environments. The relevant failure modes for the HX are considered to be pressure wall burst due to overpressure or creep-rupture, and fatigue cracking due to repeated thermal cycles combined with creep effects at high temperature. A basic assumption that went into evaluating the above failure criteria is that the steady state thermal condition is the worst case and it contains the information needed to conservatively evaluate relevant failure criteria. This assumes that the startup and shutdown transients do not create stress or strain conditions that govern the failure of the HX. Making this assumption requires only a simple steady state thermal solution and only one time point to be evaluated in the structural analysis. This simplifies and reduces the scope of the analytical effort, which is desirable when evaluating many flow conditions and design configurations.

The structural analysis was performed using a finite element model. The model, built to the dimensions of the computer-aided design (CAD) model, represents a one-twelfth slice of the HX, taking advantage of symmetry in the design. The model includes the HX, the reactor heat pipes, and partial core. The model is comprised of both 10-node tetrahedron and 8-node brick elements, totaling ≈106,000 elements. The same model was used to solve for the steady state thermal solution and the structural solution, using ANSYS version 6.1. Figure 7 depicts the model used in the analysis.

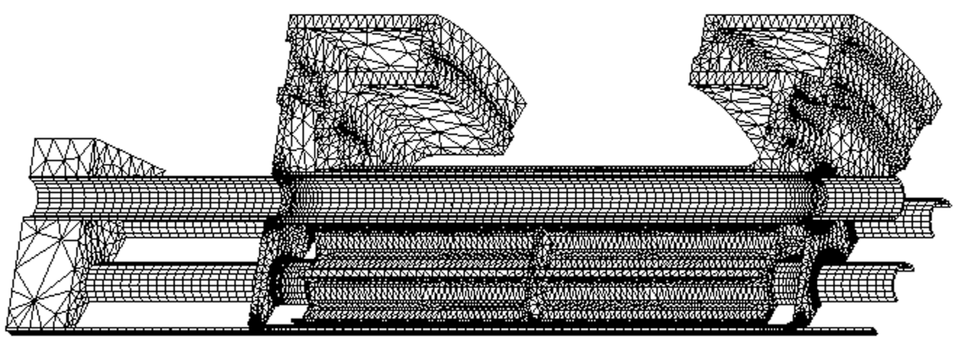

Figure 7. SAFE–100 heat exchanger finite element model.

The SAFE–100 test series is comprised of 12 proposed test conditions, each of which may be used multiple times. Therefore, the structural analysis of the HX must evaluate each condition separately and then consider the lifetime capability of the HX against the expected operational life. Table 2 lists each test condition and its expected number of thermal cycles and hours of operation.

4.1.1 Thermal Solution

The steady state thermal solution was determined from a set of thermal boundary conditions applied to the surfaces of the model. The fluid/thermal analysis described above provided the heat transfer coefficients and bulk fluid temperatures in each annular flow channel and in the inlet and outlet manifolds. The exterior of the HX and heat pipe sections between the core and HX were assumed adiabatic. With the thermal boundary conditions applied, the temperature profile was solved for each test condition for input into the structural analysis.

Table 2. Proposed test conditions.

Test No.	Gap Fill	Failed Heat Pipe	Power (kW)	Flow (kg/s)	T in (K)	T out (K)	Expected Use	
							Cycles	Hr
H1	Helium	No	27.5	0.147	760	850	14	40
H2	Helium	No	55	0.147	600	781	3	12
H3	Helium	No	65	0.174	600	781	3	12
H4	Helium	Yes	27.5	0.147	760	850	5	16
H5	Helium	Yes	55	0.147	600	781	3	12
H6	Helium	Yes	65	0.174	600	781	3	12
B1	Braze	No	27.5	0.147	760	850	14	40
B2	Braze	No	55	0.147	669	850	3	12
B3	Braze	No	65	0.174	669	850	3	12
B4	Braze	Yes	27.5	0.147	760	850	5	16
B5	Braze	Yes	55	0.147	669	850	2	8
B6	Braze	Yes	65	0.174	669	850	2	8

4.1.2 Structural Solution

The loadings for the structural solution consist of a thermal profile from the thermal analyses and an internal pressure of 1.38 MPa. In addition to the symmetry boundary conditions on the radial planes, the heat pipe nodes at the core interface were restrained to lie in that plane. Each test condition was then run to solve for the steady state stress and strain profiles.

The thermal profiles for each load case were high enough that they induced localized plastic strains. Therefore, each case was run with temperature-dependent bilinear stress-strain material models. The solution was then solved nonlinearly. Some of the load cases were also run elastically to calculate stresses due only to the pressure loading. For these cases, the coefficient of thermal expansion was set to zero to remove the thermal stress.

4.2 Structural Criteria

The structural criteria applied to the design of the SAFE–100 HX have been derived from the American Society of Mechanical Engineers (ASME) boiler and pressure vessel code (BPVC), section III, division 1—subsection NH.[4] This subsection applies to pressurized components at elevated temperatures where creep effects are significant. The criteria used to address the burst and fatigue failure modes are load-controlled stresses, total lifetime inelastic strains, and combined creep-fatigue damage.

The load-controlled stress criteria are the rules found in BPVC subsection NH–3222 for service levels A and B. These criteria are used to ensure that no short-term strength or long-term creep-induced failure occurs due to the pressure load. The criteria are based upon membrane and bending stress intensity through a section and are limited by allowable time-independent and time-dependent stresses given in the code for 316 stainless steel. The thermal stresses are not included in this criterion since they are displacement-controlled and do not contribute to an overstress type of failure.

The inelastic strain criteria are taken from appendix T–1310 of subsection NH and limit the maximum lifetime allowable local strain, including all plasticity and creep effects. The limits are 1 percent average strain through a section, 2 percent strain at the surface due to an equivalent linearized strain through a section, and 5 percent peak strain at a point based upon the maximum positive principal strain.

The creep-fatigue damage criterion is intended to assess microstructural damage that can occur due to repeated loading and creep mechanisms and eventually lead to crack initiation. The criterion from appendix T–1411 is summarized below:

$$\underbrace{\sum_{j=1}^{p}\left(\frac{n}{N_d}\right)_j}_{\substack{\text{fatigue}\\ \text{ratio}}} + \underbrace{\sum_{k=1}^{q}\left(\frac{\Delta t}{T_d}\right)_k}_{\substack{\text{creep}\\ \text{ratio}}} \le D \ , \tag{1}$$

where

n = number of applied cycles for cycle type j
N_d = number of allowable cycles for cycle type j based on low cycle fatigue life
Δt = duration of time interval k
T_d = allowable time duration for time interval k based on stress to rupture
D = total allowable damage given by figure 8 for 316 stainless steel.

The criteria used for welds and the surrounding heat-affected zones are the same as above except that lower allowables are used per the ASME code. The allowables for the load-controlled stress do not change for 316 stainless steel, but the allowable maximum inelastic strain levels are one-half the allowable level for nonwelded regions. The number of allowable fatigue cycles is one-half that of the parent material and the stress-to-rupture allowables are reduced a few percentage points for the creep-fatigue criteria.

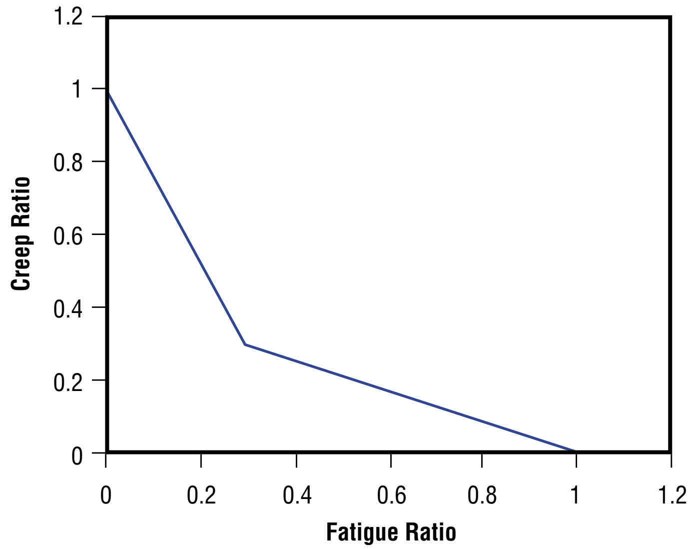

Figure 8. Allowable creep-fatigue damage ratio for 316 stainless steel.

4.3 Results

4.3.1 Test Model

The analysis for the SAFE–100 test model involved running the steady state thermal and structural solution for the 12 proposed test conditions. The stresses and strains from each case were then checked against the design criteria. Since the time for each test is relatively short (<10 hr), the effect of stress relaxation due to creep was not included. The short test times and numerous cycles act to make the life of the HX fatigue dominated.

The stresses due to the internal pressure, as illustrated in figure 9, are essentially the same for all test conditions. The peak stress occurs in the cover plates between the outer heat pipes and the outside edge weld where the unsupported span is the largest. The load-controlled criteria are based upon the membrane and bending stresses through a section. In the analyses, these stresses were extracted from the model at the peak stress intensity locations of each part. The results show that the stresses are below the ASME code strength allowables and allow for >100,000 hr of creep life.

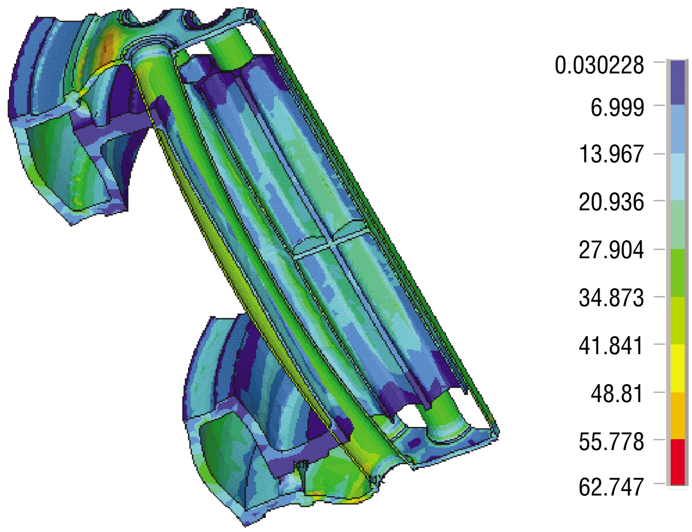

Figure 9. Stress intensity (MPa) due only to pressure.

The temperature variations in the HX cause significant thermal stress to develop. The stress in some areas is above the material yield strength so that plastic strains develop in localized regions. These regions are primarily around the weld joints at the ends of the sleeves; therefore, the criteria for welds are used. The strains in these regions are the critical factor determining the lifetime capability of the HX. The failed heat pipe cases are the most severe due to the large temperature difference between the failed heat pipe and the adjacent heat pipe. Figure 10 shows the temperature profile for a failed heat pipe condition.

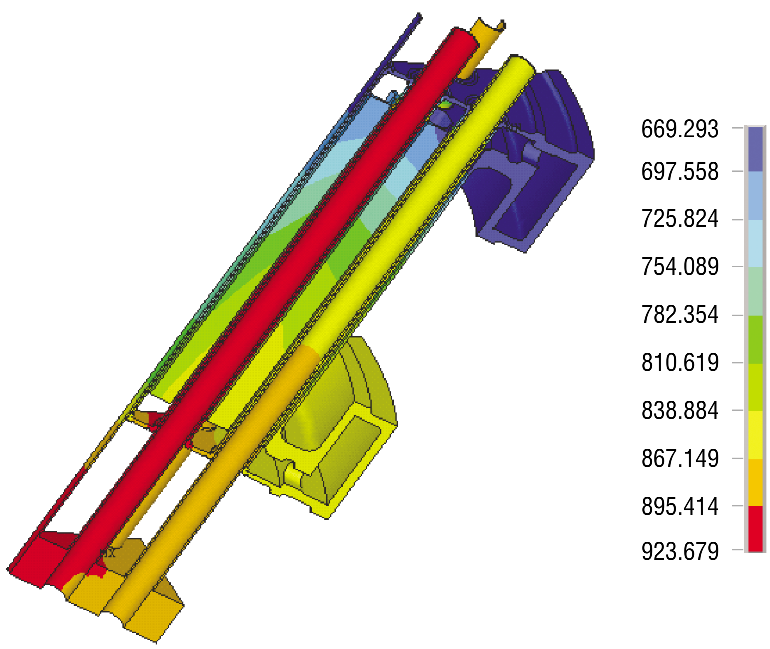

Figure 10. Temperature profile (K) for brazed, 55-kW, failed heat pipe condition.

Within the localized plastic regions, the maximum principal strain is within the inelastic strain criteria for all of the nonfailed heat pipe conditions. The failed heat pipe conditions generate higher strain levels, and two points exceed the surface strain allowable by 10 percent. The strains in these locations are within the fatigue allowables. Figure 11 shows the peak strains occurring in the center failed heat pipe sleeve on the inlet side.

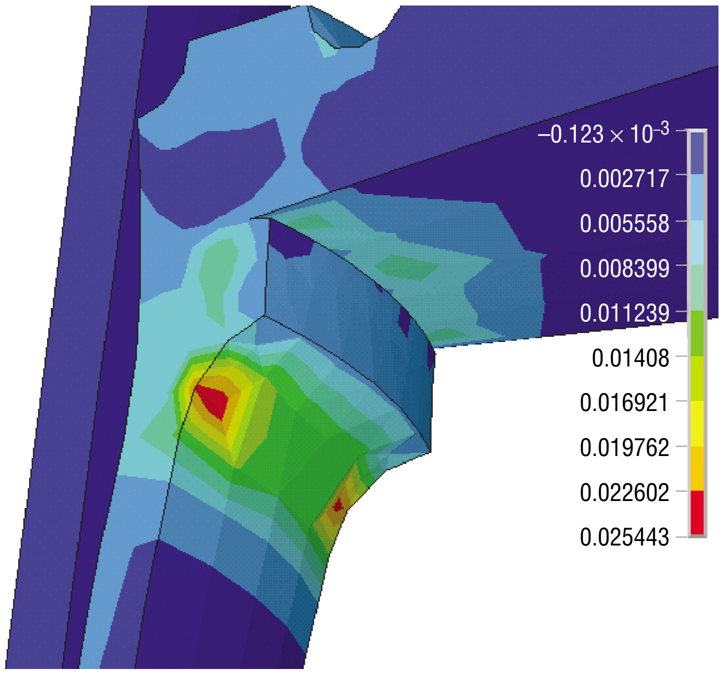

Figure 11. Maximum principal strain in center failed heat pipe sleeve.

The creep-fatigue limit requires knowledge of the temperature, stress, and strain state at a given point. For this analysis, the three nodes in the HX corresponding to the maximum temperature, equivalent total strain, and Von Mises stress were used to calculate an allowable number of cycles and creep lifetime at each node. These nodes were generally at different locations from test to test. Therefore, this is a somewhat conservative application of the criteria. The ratio of the expected usage to the allowable number of fatigue cycles and creep hours for each test condition were then summed to determine the total damage, as listed in table 2. Figure 12 plots the maximum damage locations against the allowable damage ratio for 316 stainless steel. The calculated creep damage is significant only for the outlet cover plate due to its high stress and temperature. The results for the outlet cover plate lie just beyond the allowable damage curve. By removing conservatism in the analysis, it is likely that this point would fall within the allowable region.

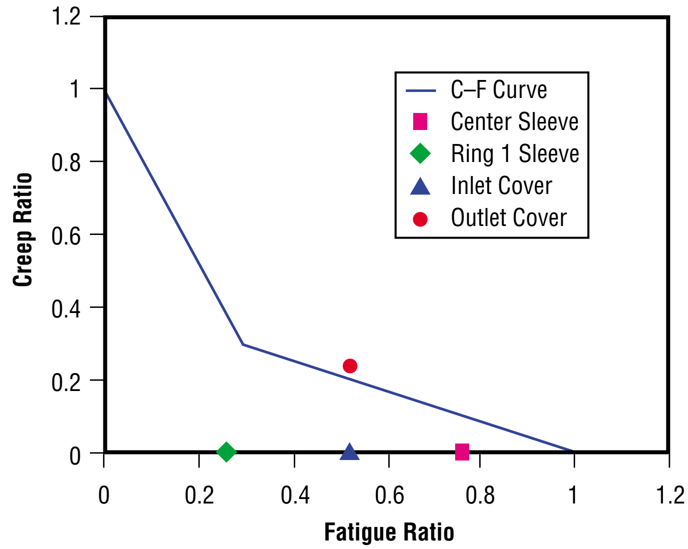

Figure 12. Creep-fatigue damage results.

Nearly all of the fatigue and creep damage occurs during the failed heat pipe tests. Figure 13 shows that the maximum strain levels in the failed heat pipe tests are 2–18 times greater than the nonfailed heat pipe tests. Additional nonfailed heat pipe test cycles could be added to the test series with negligible impact to the HX creep-fatigue damage.

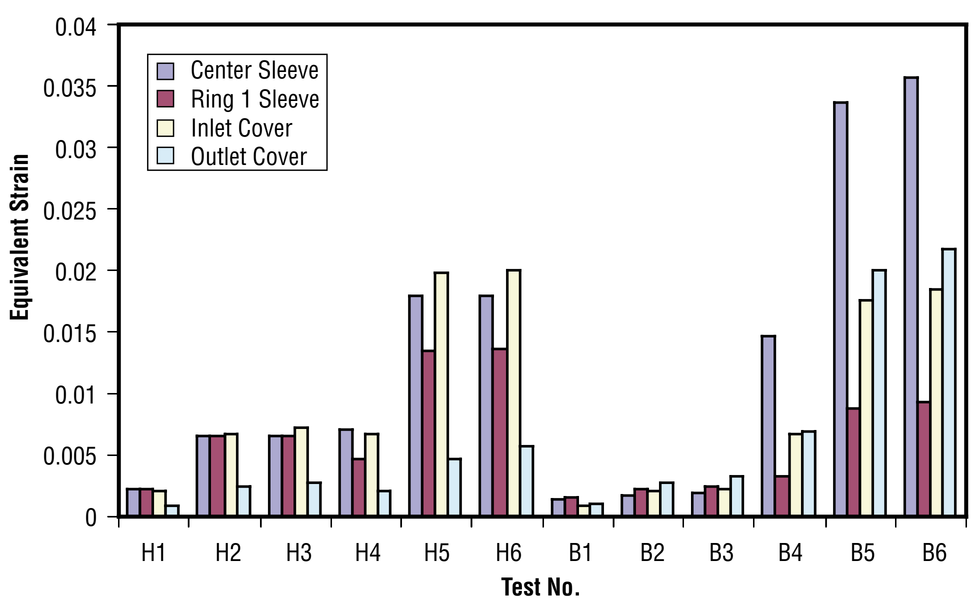

Figure 13. Maximum strain values for each test condition.

4.3.2 SAFE–100 Reactor

An analysis of the test model using flight reactor thermal conditions was performed. One purpose of this analysis is to compare with the test model results for similarity in performance in order to show how well the SAFE–100 tests represent the structural behavior of a flight unit. The results indicate that the temperature of the reactor is a few degrees cooler and that reactor stress and strain are similar to the tests. Figure 14 plots the stress and strain for a few select nodes representing the peak damage locations from the reactor for both the SAFE–100 test and reactor conditions. The reactor stress and strain tracks the test results fairly well, indicating the tests are a good representation of the reactor behavior.

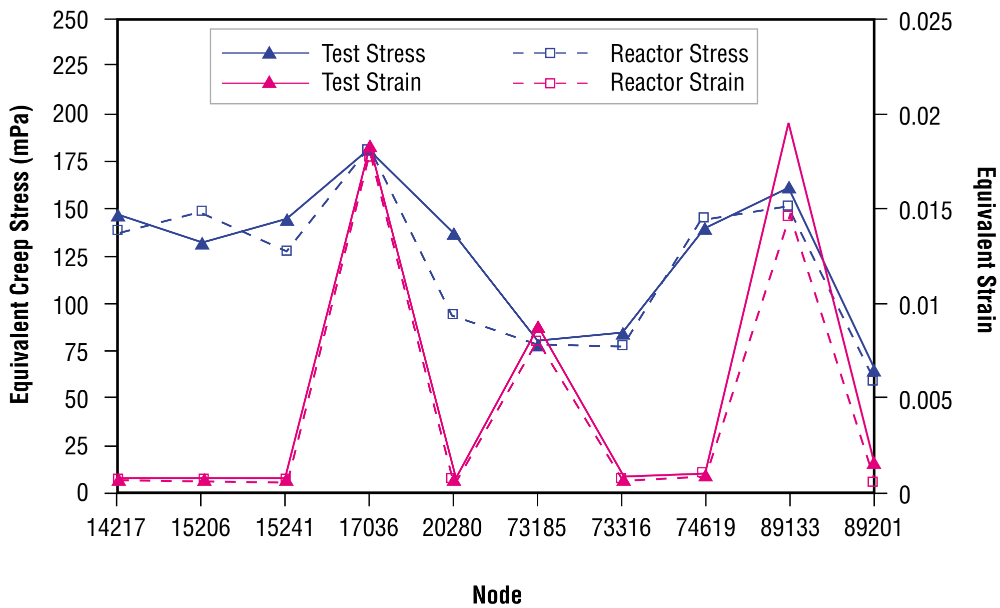

Figure 14. Reactor and test condition stress and strain levels.

Another purpose of the reactor analysis was to investigate the stress relaxation effects due to creep over a long mission life, since a flight reactor could be expected to operate for several years. Creep properties for 316 stainless steel were included in the test model. The model was then run with the reactor conditions for several time points up to 50,000 hr. All of the strain due to coolant pressure, as well as temperature differences, was included in the first time point.

The creep effects are greatest in the hot outlet side of the HX. The critical locations are again in localized inelastic regions around the sleeve to cover plate welds. The equivalent stress and strain for a peak point in the outlet cover plate are plotted versus time in figure 15. Results from both a nonfailed heat pipe and failed heat pipe condition are included. The creep effects are not significant at times <100 hr. At times >100 hr, significant strain levels begin to accumulate and the stress state experiences significant relaxation. These results serve to confirm the decision to neglect creep effects for the short-duration tests.

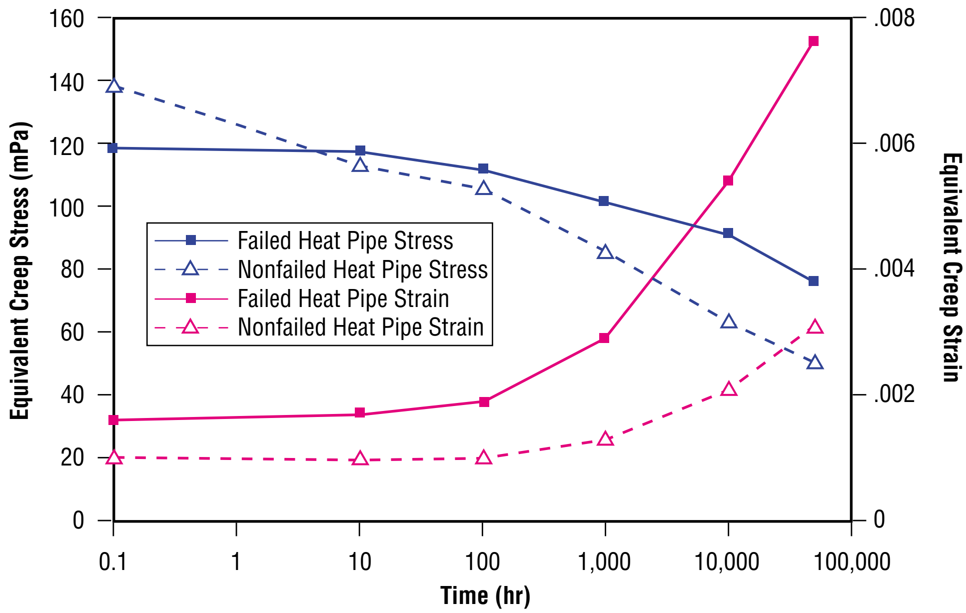

Figure 15. Stress and strain versus time for the outlet cover plate.

5. HEAT EXCHANGER POWER INCREASE POTENTIAL

The parameters affecting the amount of power that can be transferred in the HX include module dimensions and number of modules, heat pipe vapor temperature, HX temperature and stress limits, coolant pressure and pressure drop, coolant inlet and exit temperatures, and coolant gas constituents. For the SAFE–100 design, many of these parameters were considered fixed either by the nuclear design or by the Brayton system requirements. In this study, the heat pipe vapor temperature (and its effect on HX temperatures) was the only variable. For each case, the coolant annulus dimensions (width and length) were adjusted to achieve the desired heat pipe vapor temperature and a pressure drop of $\Delta P/P$=0.015 in the annulus.

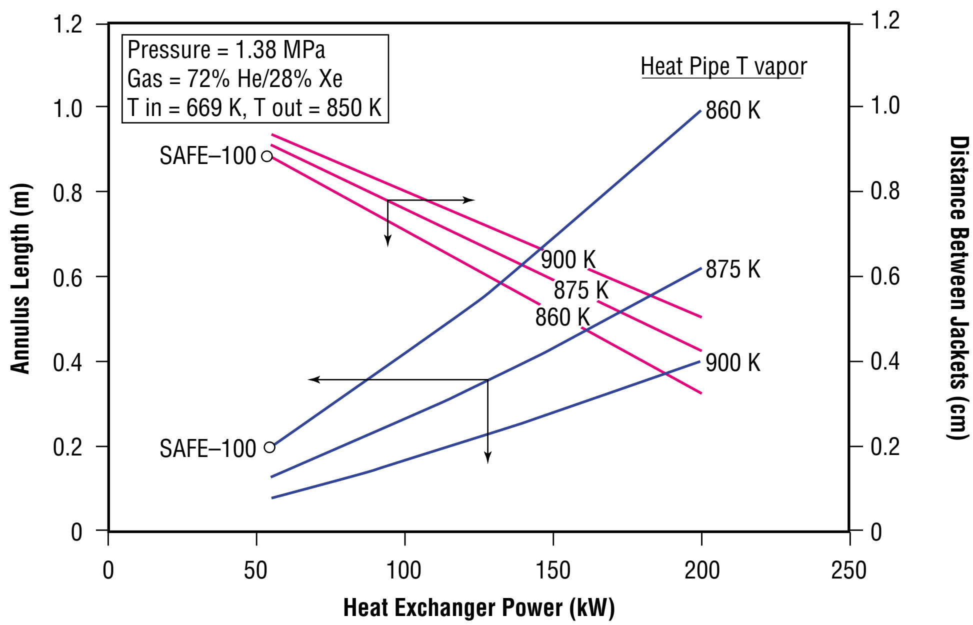

Figure 16. Heat exchanger power increase assessment.

The results of this analysis are shown in figure 16 for heat pipe vapor temperatures varying from 860 K (SAFE–100 reactor design temperature) to 900 K, and for HX powers of 55 kW (SAFE–100 HX power) to 200 kW. The parameters plotted are length of flow annulus and the spacing between adjacent flow channels. The length is important as it affects the size and weight of the HX. The spacing between adjacent flow annulus jackets provides an ultimate limit in the HX power. At a high enough power, this distance becomes zero, and further increases in power are not possible without changing one or more of the operating parameters that were fixed in this analysis.

The heat pipe vapor temperature is important because it provides the boundary condition temperature for the reactor core. This temperature is increased by shortening the annulus length (decreasing heat transfer area), which in turn decreases the annulus width needed to meet pressure drop and increases the spacing between adjacent flow annuli. Figure 16 shows that the SAFE–100 HX is capable of much higher power levels. It should be noted that these increases in power do not increase appreciably the stresses in the HX. The key drivers of stress in the HX are coolant temperature rise and coolant pressure, which are held constant.

However, at higher powers the HX becomes long, indicating that changes to reduce length should be assessed. In addition to allowing the heat pipe vapor temperature to increase, these changes include a larger temperature rise, higher coolant pressure, and/or a gas mixture with a higher mole fraction of He. The increases in coolant pressure and coolant temperature rise would increase HX stress, which would need to be evaluated. If needed, increases in temperatures could be offset by a reduction in coolant temperatures, with some attendant loss in Brayton cycle efficiency.

6. CONCLUSIONS

The structural performance of the HX is driven by the thermal stress and strain. The sleeve-to-cover plate weld regions are the most critical locations because of the peak localized stresses and the lower weld allowables. This makes the quality of these welds an important manufacturing issue.

For the SAFE–100 test series, the HX meets the strength criteria of the ASME BPVC. The failed heat pipe conditions are much more severe and create local strains and stresses that exceed BPVC allowables for inelastic strain and creep-fatigue damage at local points. These violations are not great, however, and are not considered to affect the SAFE–100 test objectives.

The SAFE–100 test environment produces a stress-strain state similar to the reactor conditions. The tests are therefore a good simulation for a flight unit. The analytical techniques developed for the SAFE–100 tests can now be used to support tailoring the HX design to meet the requirements of a long mission life.

Should a mission require a reactor power >100 kW, this same HX design could be used up to certain physical limits by adjusting the annulus length, width, and/or heat pipe vapor temperature. This can be done without altering the pressure and coolant temperature.

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13. ABSTRACT *(Maximum 200 words)*
A heat pipe-cooled reactor coupled to a Brayton cycle is currently under consideration for nuclear electric propulsion or as a planetary surface power source. In this system, power is transferred from the heat pipes to the Brayton gas via a heat exchanger attached to the heat pipes. This Technical Memorandum (TM) discusses the fluid, thermal, and structural analyses that were performed in support of the design of the heat exchanger to be tested in the Safe, Affordable Fission Engine experimental program at Marshall Space Flight Center. A companion paper, "Mechanical Design and Fabrication of a SAFE–100 Heat Exchanger for use in NASA's Advanced Propulsion Thermal-Hydraulic Simulator," presents the fabrication issues and prototyping studies that, together with these analyses, led to the development of this heat exchanger. An important consideration throughout the design development of the heat exchanger was its capability to be used for higher power and temperature applications. This TM also discusses this aspect of the design and presents designs for specific applications under consideration.

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